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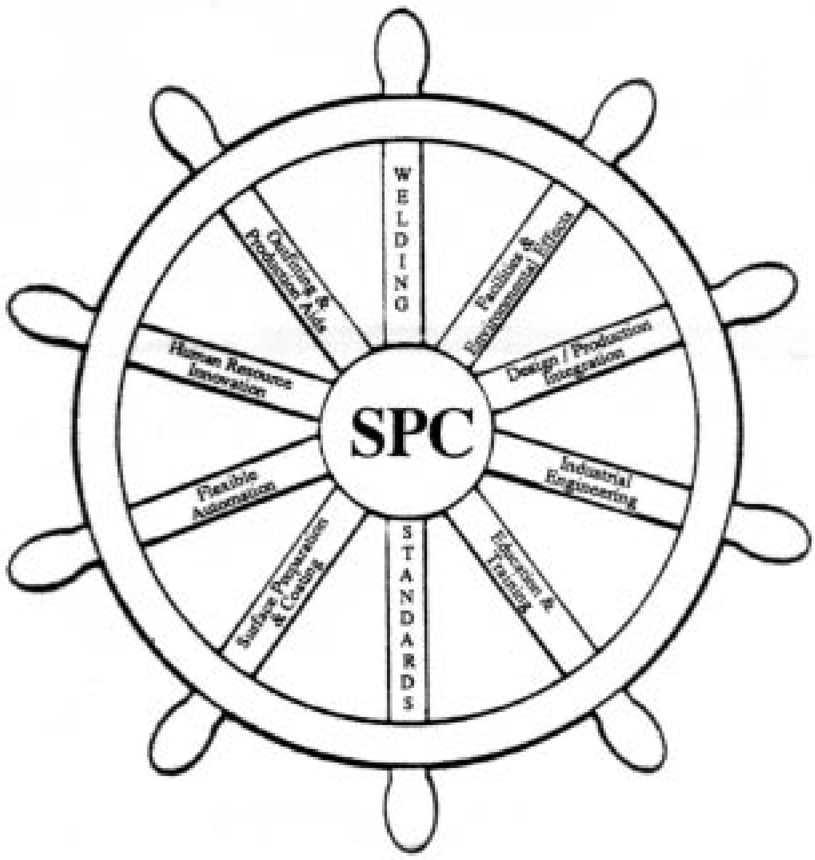

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Shipboard Aluminum/Steel Welded Transition Joints Evaluations and Improvements

5B-2

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ABSTRACT

Aluminum to steel explosion welded transition joints are used to attach aluminum superstructures to steel hulls. Transition joint bond separation sometimes occurs during ship construction. Ingalls Shipbuilding conducted a long term study to determine causes and corrective action for these separations.

The aluminum/steel transition joints are manufactured by the explosion bonding process and tested in accordance with MIL-J-24445. Traditional transition joints consist of alloyed aluminum bonded to mild steel with an interlayer of low alloy aluminum.

The study reviewed transition joint manufacture and quality testing required by the material specification, reviewed the adequacy of design guidelines and production practices, and considered cost effective methods for corrective action. Modifications in product design and testing, installation design and shipyard production practices can improve reliability. The most important result of this study was development of material with improved Properties. This paper relates the study procedure, findings and recommendations so that transition joint separations can be avoided on future installations. This information is useful for designers and transition joint users.

DESCRIPTION

Aluminum cannot be arc welded directly to steel because of metallurgical incompatibility. Aluminum to steel welds can be produced using cold welding processes, such as explosion welding (EXW). Conventional fusion welding processes then can be made to attache the EXW transition to respective compatible metal components. This combination provides a crevice free, fully welded joint between aluminum and steel. This is a significant advantage over mechanical fastening by riveting or bolting.

The aluminum to steel transition joints typically are welded to a steel coaming about five inches above the topmost steel deck. The transition joint supports the bulkhead plating, vertical stiffeners and framing. The bond surface is parallel to the deck. See Figure 1 for a typical design. Earlier designs used 3.5 cm (1-3/8 inch) thick transition joints. Recent designs use 2cm (3/4 inch) thick transition joints.

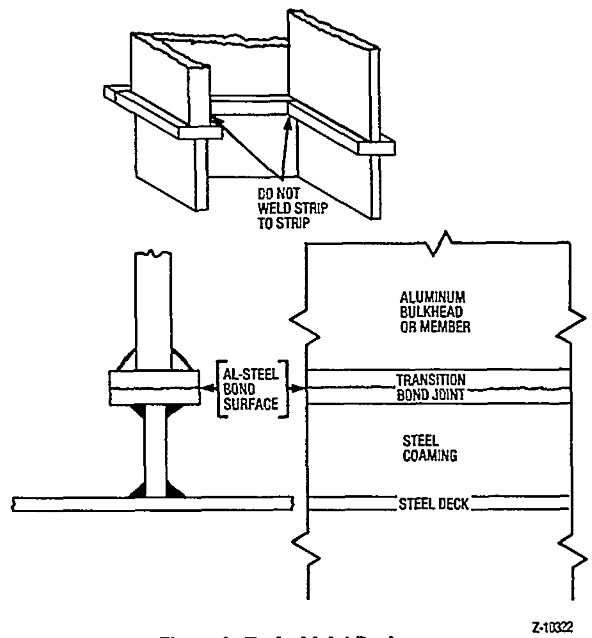


Figure 1. Typical Joint Design

In earlier years, there were few reports that bond separation had occurred. Recently, some bonded joints separated as a result of normal operations in high sea state conditions. These disbonds resulted in closely focused attention on all bond joints. The separations were puzzling because these transition joints were designed to be the "strong link" in the structural chain (stronger than the aluminum plating welded to the joint).

Ships under construction were closely examined. For about a year, locations of disbond repairs were monitored to analyze why the disbonds were occurring. The findings are discussed later in this paper. The study showed that disbonded lengths were generally short, typically 15-30cm (six to twelve inches) long. Occasionally, longer pieces were replaced because there were several adjacent repairs. All known disbonded locations have always been repaired before any ship left the shipyard. Disbonding in service is rarely reported, so apparently disbond in fleet service is unusual.

Transition Joint Manufacture

Aluminum to steel bonded transition joints are manufactured in accordance with the requirements of MIL-J-24445. Although the specification permits manufacture by several processes, the only process currently used for manufacture of shipboard transition joints in the USA is explosion welding. The basic explosion bonding process is essentially the same for all producers. Reference (1) provides a thorough description of the technology and of the development of aluminum to steel transition joints for shipboard applications. Figure 2 depicts the basic explosion bonding process. The plates to be bonded are fixtured parallel to each other and separated by a gap. A layer of explosive is placed on top of the upper plate. The explosive is a formulation specifically manufactured for explosion bonding; the detonation rate is typically in the 2500 to 3000 meter/second range. The explosive detonation initiates at the edge of the plate, the initiation point. Upon initiation, the detonation front travels across the plate at the detonation rate of the explosive. The gas expansion resulting from detonation accelerates the upper plate downward causing the plates to impact at an angle, typically in the 15 degree range. At the point of impact, surface pressures of several million PSI are developed. These pressures create a spalling condition on the surfaces out in front of the collision point. This spalling condition, or jetting, strips the surfaces clean of oxides and surface contaminants immediately prior to collision. At the collision point, the newly cleaned surfaces are driven into intimate metallurgical contact, resulting in metallurgical welding of the two plates. Under the high pressures and high velocities, a waveform develops at the bond, providing a unique "footprint" exclusive to the explosion welding process. Figure 3 is a cross section of this footprint. During this operation, there is essentially no heat generated at the bond zone. Consequently, this cold welding operation is suitable for jointing materials that cannot be fusion welded, such as aluminum and steel.

If bonding parameters are correct, the explosion bonded plate exhibits relatively uniform strength throughout. A slight reduction in strength may be

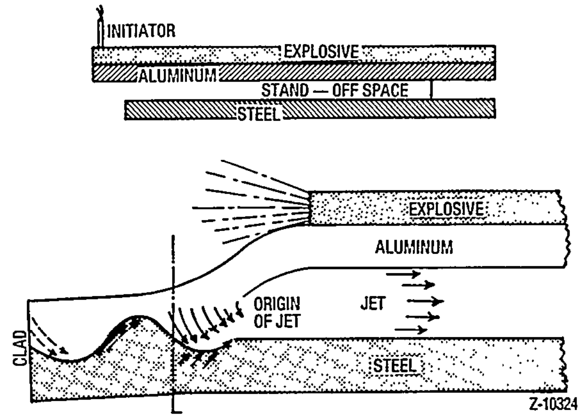


Figure 2. Parallel arrangement *for explosion* cladding and subsequent collision between the prime and *backer metals that leads to jetting and formation of wavy bond zone.*

Figure 3. Photomicrograph steel to aluminum bond (edge) showing a characteristic wave swirl with a small intermetallic pocket at the "crest".

observed near the initiation point. When aluminum is bonded directly to steel, there are isolated pockets of low strength material in the wave swirl, Figure 3. These melt pockets are normally fully surrounded by high strength, ductile material. Significant reductions in overall bond strength can occur if the explosive detonation rate is not adequately controlled. The strength reductions can be associated with areas of melt along the bond zone outside the isolated melt pockets. In general, the permissible range of detonation rates is broad, and proper control of bond uniformity is not an issue.

In the early explosion bonding development work discussed in Reference (1), it was observed that a direct explosion weld between aluminum 5000 series alloys and steel exhibited low strength and poor toughness. The deficiency was corrected by insertion of an interlayer of unalloyed aluminum, type 1100, between the marine grade aluminum and the steel. The original 3.5cm (1-3/8 inch) thick transition joints consist of thick 5456 aluminum alloy bonded to an interlayer of 0.375 inch thick 1100 aluminum and a base of 0.75 inch steel. Later 2cm (3/4 inch) thick transition joints were made using 0.125 inch thick 5456 or 5086 aluminum alloy bonded to a 0.25 inch thick interlayer of 1100 aluminum and a base of 0.375 inch steel. Although these products are actually comprised of three alloy layers, they are commonly referred to as "bimetallic" transition joints.

Transition Joint Quality Testing

Aluminum to steel welding transition joints are quality tested in accordance with the requirements of MIL-J-24445. This specification requires ultrasonic inspection of every plate. In addition, one plate from every lot, or 1 in 10, whichever is more frequent is mechanically tested. Test specimens are to be cut from two diagonally opposite corners of the selected plates. Either a ram tensile test and a side bend test, or a ram tensile test, bond shear strength test and a chisel test are required. Before testing, samples are heat treated 15 minutes at 600 degrees F. to simulate the "as welded" condition. Specification requirements are: 8,000 PSI minimum shear strength; 11,000 PSI minimum tensile strength; and no bond failure in either the side bend test or the chisel test. All plates are inspected over 100% of the surface by straight beam ultrasonic inspection to detect areas of non-bond. Although ultrasonic testing will reliably detect non-bond, it will not reliably detect areas of low bond strength.

Bond Separations Study

The objective of the study was to determine the cause of disbonding and implement preventative measures. Numerous possible causes of shipboard transition joint disbonding were considered and studied. The study primarily concentrated on the following questions:

(1) Is the bonded transition joint being overheated during shop and field welding?
(2) Are oversize weld fillets causing excessive or uneven stress?
(3) Are there differences between earlier and current materials?
(4) Does the bond material meet MIL-J-24445?
(5) Does MIL-J-24445 provide adequate control over the material?
(6) Are improvements beyond MIL-J-24445 minimums feasible?
(7) Are guidelines for, and design widths of, strips adequate?
(8) Is the restraint effect of the ship's structure significant?
(9) Can the reliability of aluminum-steel transitions be improved?

Examinations of disbonded strips showed all separations occurred at the bond between the 1100 alloy aluminum and the steel. No separations were observed at the 5456 to 1100 alloy aluminum bond. Separated strips showed the characteristic wavy bond surface pattern associated with properly bonded material. Most welds attaching the transition joints to superstructure had large aluminum fillets that typically came out to the edge of the strip. Most steel fillet welds attaching the transition joint to the steel coaming were smaller and did not come to the edge. Most disbonded locations included or were adjacent to butts in strips. Examination of separated locations revealed several cases where full penetration butt joint designs were used instead of the partial penetration butts recommended in Reference (2) and shown in Figure 4. Because aluminum cannot be arc welded to steel, full penetration butt joints are not possible. Full penetration butt joints joining two strips result in local overheating and weakening of a short length of bond funder one inch). Depending on stresses applied to the butt, this small area may initiate global disbond growth through

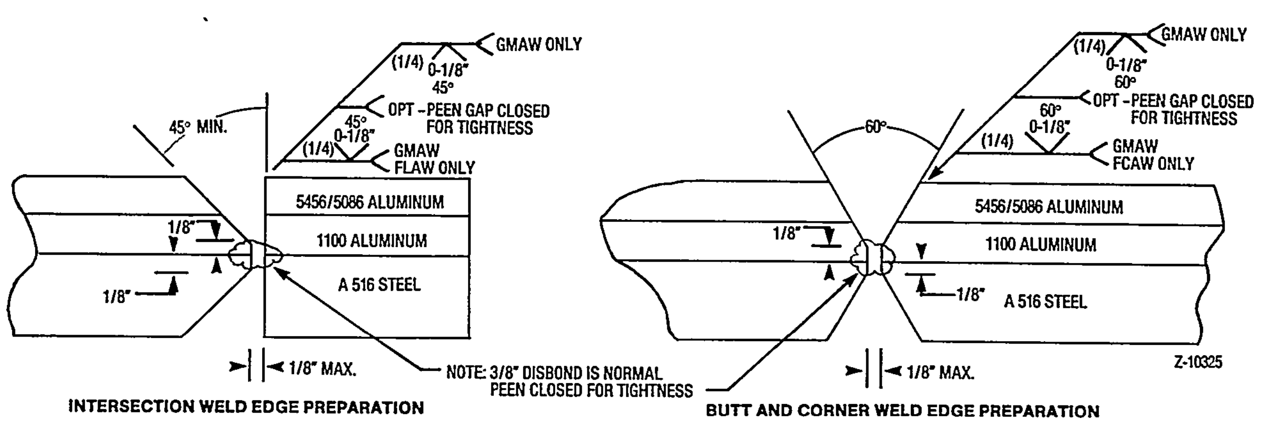


Figure 4. Recommended butt joints in transition strips

peeling. Although the design drawings may show a full penetration symbol, production craftsmen were instructed to use only partial penetration butt welds in transition joints.

Disbonded samples were analyzed by Ingalls, two vendors, and a Navy laboratories. Our chemical lab verified that the proper alloys were used in each layer. Other samples were sent to the two primary transition joint suppliers and to an independent Navy laboratory. Based on microscopic examination of the bond, the joint manufacturers and the Navy laboratory concluded that there was a possibility that the transition joint had been overheated (probably during welding). Based on this preliminary finding, production welding of transition joints to ship's structure was switched from spray arc (flux cored for steel) to pulsed GMAW (Gas Metal Arc Welding). This change was made to lower heat input and peak temperature at the bond zone.

Evaluation Of Welding Heat Input Effects

Since aluminum and steel are not metallurgically compatible at elevated temperatures, the aluminum to steel bond zone can be degraded by excessive heating. Transition joint manufacturers recommend that bimetallic aluminum to steel transition joints not be heated over 315°C (600°F). during installation. Table I presents strength data for bimetallic transition joints under various thermal conditions. Note, there are significant reductions in strength after relatively short time excursions above 371°C (700°F). Also, note that the strength of the product is significantly reduced when tested at elevated temperatures.

Tests were conducted in our welding lab to determine the temperature at the

TABLE I
TEMPERATURE EFFECTS ON TRANSITION JOINT STRENGTH

MAT'L	PEAK TEMP	TEST TEMP	RAM TENSILE
BI	70'F	70'F	14,527
BI	600'F	70'F	12,221
BI	800'F	70'F	7,150
BI	1000'F	70'F	3,812
BI	600'F	300'F	9,600
BI	600'F	600'F	6,215
TRI	70'F	70'F	26,839
TRI	600'F	70'F	23,140
TRI	1000'F	70'F	18,012

BI = BIMETALLIC JOINT
TRI = TRIMETALLIC JOINT

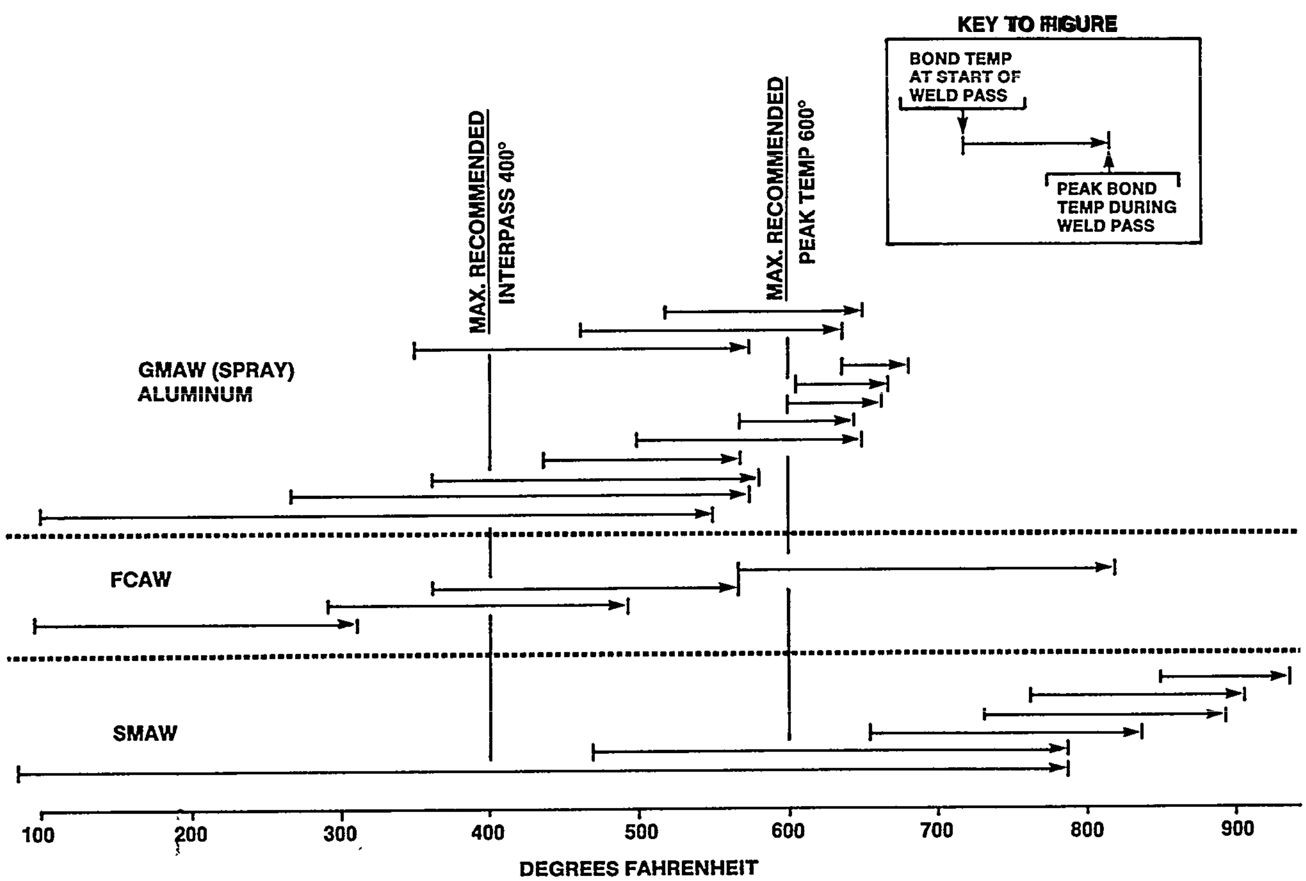


Figure 5. Weld interpass and peak temperatures

bond during various weld processes. Holes were drilled from the back side to the bond joint, and digital thermocouples were used to measure temperatures. A spray arc aluminum single pass weld resulted in a peak bond joint temperature of 274°C (525°F) for 2cm (3/4 inch) strips and 254°C (490°F) for the 3.5cm (1-3/8 inch) thick strips. A 12 pass full penetration aluminum weld with NO cooling period between passes resulted in a worst case peak bond joint temperature of 343°C (650°F). Aluminum welding tests showed that if the interpass temperature was below 204°C (400°F), peak temperature at the bond joint was below limiting temperature of 315°C (600°F), regardless of process.

Steel welding resulted in considerably higher temperatures at the bond. Multi-pass (no interpass cooling) with SMAW (shielded metal arc welding) process resulted in 499°C (930°F) at the bond joint. A similar weld with FCAW (flux core arc welding) process resulted in a peak temperature of 435°C (815°F), but, compared to SMAW, a shorter duration above the limiting temperature. SMAW is clearly unsuitable for welding transition joints within the allowable bond joint temperature envelope. Both semi-automatic GMAW (gas metal arc welding) and FCAW complying with the 204°C (400°F) interpass temperature limit resulted in acceptable peak temperatures at the bond surface. Figure 5 graphically illustrates the relationship between process, interpass, and peak temperatures.

The welding lab's tests showed that production fillet welding probably did not overheat the joint. SMAW could have caused thermal degradation. However, SMAW was not used for this application. Most of the separations were observed where single pass fillets were used. Based on this data, the production welding processes in use were not the likely cause of disbonding. As a preventative measure, though, welding continued with the pulsed arc GMAW process, which has the least heat input of any process permitted by the ship's specifications. One of the manufacturers recommended short circuiting GMAW for the steel attachment weld. Short arc is an even lower heat input process than pulsed arc. However, the short arc process is not permitted in this application by the U. S. Navy's specifications.

Production experiences corroborated the laboratory test results above. Production welding clearly showed that complying with temperature limits would not prevent separation. Hulls welded entirely with pulsed GMAW process did not show noticeable change *in transition* joint reliability. Bulwarks on one of the ships were field welded in strict compliance with temperature limits. Every welding pass was monitored by shipyard and Navy Quality Assurance inspectors. Still, disbonding was found after depositing *as* few as three passes. In another case, welding of aluminum to a transition joint in a shop was closely monitored. This configuration of the deck pad permitted Ultrasonic Testing (UT) before and after welding the aluminum to the transition. Several internally disbonded areas were detected by UT after welding (internal disbonds cannot be found visually). Cross sectioning of the pad confirmed separation at the indications. This occurrence was later duplicated under laboratory conditions. Similar production experiences indicated the need to look for other possible causes.

Transition Joint Manufacturer Visits

Visits to the transition joint manufacturer's facilities were arranged as part of our shipyard's regular program of supplier review. Both manufacturers had been involved in the study since it's inception, and each visited the shipyard to review end use of the product. Separated samples had been provided to both, and their analysis welcomed. Discussions during the visits also provided further avenues for study and improvements. Both manufacturers demonstrated that they were in compliance with the testing and quality control requirements of MIL-J-24445. Additional testing demonstrated that the test program of MIL-J-24445 may not reliably prevent lower strength bond material from reaching the ships. To some extent, bond manufacturers recognize this. For example, they routinely trim material beyond the areas rejectable under MIL-J-24445.

MIL-J-24445 requires test samples to be taken from opposite corners of a sample transition plate. However, weaker or more brittle bonds are usually found adjacent to the explosion initiation point. Test samples were taken adjacent to the initiation point (not a location required by MIL-J-24445 to be sampled) of plates which had already proven adequate at the required test locations. The samples taken near the initiation point showed noticeable reduction in bond strength and increased susceptibility to disbonding during "chisel testing," a qualitative test of bond ductility. Areas that far exceeded the minimum tensile and shear values failed chisel testing. Also, areas that passed the ultrasonic test criteria had bond strength below specification *requirements*.

Strip Widths

One possible way to compensate for lower actual strength of transition strips is to widen the strips. Wider strips improve the safety margin. Increasing the minimum width will also increase standardization (by deleting the narrower sizes) for improved producibility. This recommendation from one of the manufacturers lead to a study of strip widths.

Reference (2) recommends sizing joints as follows:

"a rule of thumb for joining aluminum and steel plate is to use a transition bar four times as wide as the thickness of the aluminum being welded to it. In general, the 4-to-1 rule is conservative and recommends a transition joint larger than is actually needed."

The minimum ultimate tensile strength of 5456-H116 aluminum alloy 6527kPa (45,000 PSI) is 4.09 times the minimum ultimate tensile strength of transition joint required by MIL-J-24445 1595kPa (11,000 PSI). Most of the known disbonds were found in strips that were in compliance with the 4-to-1 rule of thumb. Tests of production welded material demonstrated tensile values exceeding the requirements of MIL-J-24445. In theory, failure should always occur in the weaker bulkhead plating, never in the stronger bond. As the aluminum is not designed to reach ultimate stress under design loads, 4-to-1 should be conservative. Based on the number of disbonds experienced, this supposition must not be correct. What is the cause?

Perhaps statistics can help answer this question. The bond quality testing performed by the manufacturers show properties exceeding the minimum required by MIL-J-24445. The aluminum bulkhead plate is also stronger than the minimum. What ratio statistically provides a reliable bond joint stronger than the aluminum?

Testing was performed using welded tensile test specimens of the general design for first article testing by MIL-J-24445. It is important to note that these tests are very different from the ram tensile tests performed by the bond manufacturers. The width ratio of the transition strip and the aluminum plating were altered to assure failure of the explosion bond. The complete listing of all tests with separations in the transition strip is in Appendix A. Table II shows a summary of the strengths of strips welded to represent typical production practices. The average (50 percentile) strength of the welded strips is about 2172kPa (14,978 PSI). The average strength of our aluminum bulkhead plating is 7411kPa (51,100 PSI). If the ratio of these stresses is used (3.4 to 1) to size strip width to aluminum plate thickness, at ultimate load, the aluminum base material or weld will fracture half the time. The rest of the time, the bond will separate (assuming 100% efficient welds).

However, the average does not tell the whole story. To account for scatter, the average and standard deviation are used to calculate the 99 percentile stress, based on standard random (bell curve) distribution of the entire population. The 99 percentile stress (MIN99) is the expected value where 99% of the ultimate stresses measured will be above that point, and 1% will be below. If we use the average aluminum plate ultimate tensile stress divided by the 99 percentile strength of the bond joint (12,050 PSI based on the data from Table II) to get a recommended ratio of 4.24 to one, we can expect about one percent of the failures types to be bond separation. This is very close to the actual separation rate experienced for ships under construction during the study. If this degree of reliability is not acceptable, the ratio can be markedly reduced if the tensile strength of the bond joint is improved as discussed later in the paper.

Effects of Installation Restraint On Bond Strength

One hypothesis for the cause of bond separation was that the restraint provided by surrounding ship's structure during construction (but not in laboratory testing) could produce residual stresses that would lower effective bond strength and contribute to separation. As the aluminum has a low modulus of elasticity, stress may concentrate in the more rigid bond joint (steel has a higher modulus). Furthermore, restraint causes thermal shrinkage stresses to be applied to the bond joint while the bond is at an elevated temperature due to welding. Tensile strength of the 1100

TABLE II
SAW CUT BIMETALLIC
BOND STRENGTHS

SEQ NUM	ULT STRESS	
5	N/A	
10	12,855	
14	13,590	RESTRAINED
16	13,965	SAMPLE
18	16,084	STATISTICS
19	15,880	AVG =14,978
21	16,251	STD DEV= 1,257
23	16,286	MIN99 =12,050
28	16,124	
32	16,655	
38	14,508	
40	14,268	
41	13,978	
43	14,677	
46	12.057	
47	14,873	
49	14,336	
51	14,149	
53	14,293	
26	15,920	
30	16,481	
34	16,168	
36	16,123	

alloy aluminum and the bond strength decreases as temperature increases. Data for temperature effects on bond strength is shown in Table I and figure Figure 6. A restraint fixture rigidly held some test pieces while welding. Welding with structure restrained reduced the 99 percentile ultimate stress (MIN99 in Table III) about 10%, but did not cause any immediate separations. Pertinent test data from Appendix A is summarized in Table III.

Maximum Interpass Temperature

Another hypothesis for separation was that the peak temperature limits were set too high. Test pieces with reduced interpass temperature were welded in the restraint fixture to see if the 315°C (600°F) peak limit recommendation was too optimistic. Reducing the interpass temperature from 204 to 66OC (400 to 150°F) showed about 10% improvement in the average ultimate bond stress and 16% improvement in the 99 percentile stress. Pertinent test data from Appendix A is summarized in Table IV.

Effect of Welds Transverse To Bond

Disbonds frequently had been associated with butts in transition joints, which. are usually found at butts and intersections in bulk was again used to study the possibility that plating welds

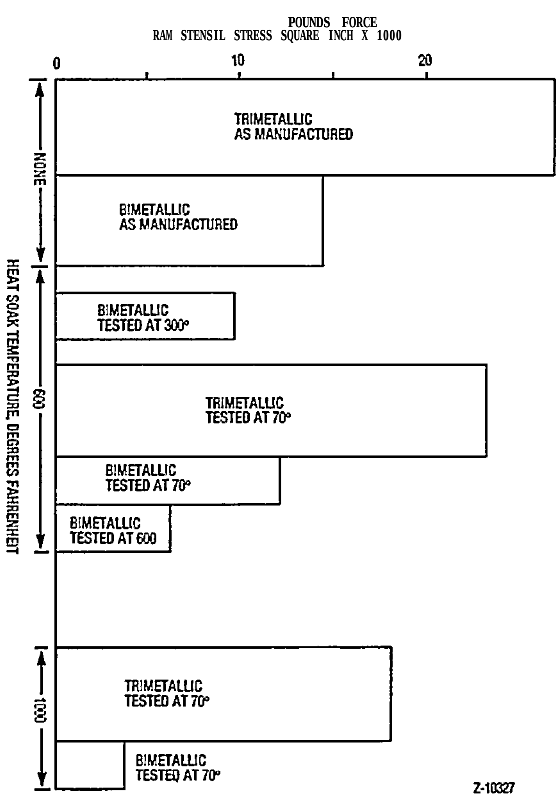


Figure 6. Heat effects on bond strength

TABLE III
EFFECT OF RESTRAINT DURING WELDING ON BOND STRENGTH

SEQ NUM	REST- RAINT	ULT STRESS	SEQ NUM	REST- RAINT	ULT STRESS
5	YES	N/A	13	NO	15578
10	YES	12855	15	NO	16860
14	YES	13590	17	NO	15775
16	YES	13965	20	NO	16518
18	YES	16084	22	NO	15930
19	YES	15880	24	NO	15946
21	YES	16251	25	NO	16287
23	YES	16286	31	NO	15939
28	YES	16124	37	NO	14773
32	YES	16655	39	NO	14429
38	YES	14508	42	NO	15765
40	YES	14268	44	NO	15179
41	YES	13978	45	NO	13615
43	YES	14677	48	NO	15012
46	YES	12057	50	NO	13918
47	YES	14873	52	NO	14676
49	YES	14336	54	NO	14914
51	YES	14149	27	NO	15263
53	YES	14293	29	NO	14196
26	YES	15920	33	NO	16198
30	YES	16481	35	NO	16742
34	YES	16168			
36	YES	16123			

RESTRAINED STATISTICS		UNRESTRAINED STATISTICS	
AVG =	14978	AVG =	15405
STD DEV =	1257	STD DEV =	898
MIN99 =	12050	MIN99 =	13313

TABLE IV
EFFECT OF MAX WELDING TEMP ON BOND STRENGTH

SEQ NUM	ULT STRESS	INTERPASS MAX TEMP	SEQ NUM	ULT STRESS	INTERPASS MAXTEMP
5	N/A	150`F	42	15765	400`F
10	12855	150`F	43	14677	400`F
13	15578	150`F	44	15179	400`F
14	13590	150`F	45	13615	400`F
15	16860	150`F	46	12057	400`F
16	13965	150`F	47	14873	400`F
17	15775	150`F			
18	16084	150`F			
19	15880	150`F			
20	16518	150`F			
21	16251	150`F			
22	15930	150`F			
23	16286	150`F			
24	15946	150`F			
25	16287	150`F			
26	15920	150`F			
27	15263	150`F			
28	16124	150`F			
29	14196	150`F			
30	16481	150`F			
31	15939	150`F			
32	16655	150`F			
33	16198	150`F			
34	16168	150‘F			
35	16742	150`F			
36	16123	150‘F			

BOO'DEG. STATISTICS	
AVG =	14361
STD DEV =	1216
MIN99 =	11529

150 DEG. STATISTICS	
AVG =	15744
STD DEV =	996
MIN99 =	13423

transverse to the bond were causing residual welding shrinkage stresses sufficient to partially disbond or weaken the transition joint. The average ultimate stress with a plate butt transverse showed a 1% improvement, and a 7% improvement in the 99 percentile stress. Pertinent test data from Appendix A is summarized in Table V.

The above results contradict logic and the actual increase of disbond frequency at butt welds. In later tests, 10mm (3/8") diameter holes were drilled through the bond zone about 1/2 inch from each end of the sample. The radius notched samples separated at 12.4% lower average stress. Pertinent test data from Appendix A is summarized in Table VI. This supports the conclusion that the transition strip butt weld geometry

TABLE V
EFFECT ON BOND STRENGTH OF PLATE
BUTT WELD TRANSVERSE TO BOND

CG53 MATERIAL, 150'F. INTERPASS

SEQ NUM	PL BUTT	ULT STRESS	SEQ NUM	PL BUTT	ULT STRESS
5	NO	N/A	26	YES	15920
10	NO	12855	27	YES	15263
13	NO	15578	29	YES	14196
14	NO	13590	30	YES	16481
15	NO	16860	33	YES	16198
16	NO	13965	34	YES	16168
17	NO	15775	35	YES	16742
18	NO	16084	36	YES	16123
19	NO	15880			
20	NO	16518	WITH XVERSE BUTT		
21	NO	16251	AVG =15,886		
22	NO	15930	STD DEV = 755		
23	NO	16286	MIN99 =14,126		
24	NO	15946			
25	NO	16287	W/O XVERSE BUTT		
28	NO	16124	AVG =15,678		
31	NO	15939	STD DEV = 1,085		
32	NO	16655	MIN99 =13,149		

TABLE VI
EFFECT OF ROUNDED
NOTCH ON BOND STRENGTH

ALL MATERIAL IS TRIMETALLIC

SEQ NOM	NOTCH	ULT STRESS	SEQ NUM	NOTCH	ULT STRESS
70	YES	21,594	66	NO	22,119
73	YES	20,102	69	NO	26,006
74	YES	20,192	71	NO	26,544
78	YES	20,991	72	NO	28,201
79	YES	23,400	76	NO	25,581
83	YES	26,041	77	NO	26,799
84	YES	22,744			
87	YES	29,077			

NOTCH STATISTICS	NORMAL STATISTICS
AVG = 23,018	AVG 25,875
STD DEV= 2,930	STD DEV 1,867
MIN99 = 16,190	MIN99 21,524

contributes to the frequent separations in this region (in addition to the temperature effects already known).

Old Vs.Current Bond Material

Reports that disbonding had *only* recently increased led to a study of whether the strength of the materials had changed. During the manufacturer visits discussed earlier, testing records were examined, but did not support a significant decrease in material properties with time. Several feet of transition joint material manufactured 3-5 years earlier than the current material was located and tested. There was only a 7% difference between old and current material averages, but the 99 percentile stress actually improved. Pertinent test data from Appendix A is summarized in Table VII.

ALTERNATIVE TRANSITION JOINT MATERIALS

The above studies clearly indicated that an increase in minimum bond strength and an increase in permissible weld temperatures would be beneficial. It is well known among explosion bond manufacturers that the insertion of a thin titanium interlayer between

TABLE VII
BOND STRENGTH VARIATION WITH
TIME (ABOUT 4 YEARS APART)

SEQ NUM	ULT STRESS	HULL MATL	SEQ NUM	ULT STRESS	HULL MATL
5	N/A	CG53	37	14773	CG65
10	12855	CG53	38	14508	CG65
13	15578	CG53	39	14429	CG65
14	13590	CG53	40	14268	CG65
15	16860	CG53	41	13978	CG65
16	13965	CG53	48	15012	CG65
17	15775	CG53	49	14336	CG65
18	16084	CG53	50	13918	CG65
19	15880	CG53	51	14149	CG65
20	16518	CG53	52	14676	CG65
21	16251	CG53	53	14293	CG65
22	15930	CG53	54	14914	CG65
23	16286	CG53			
24	15946	CG53	CG 65 STATISTICS		
25	16287	CG53	AVG = 14.438		
26	15920	CG53	STD DEV = -336		
27	15263	CG53	MIN99 = 13,655		
28	16124	CG53			
29	14196	CG53			
30	16481	CG53			
31	15939	CG53			
32	16655	CG53			
33	16198	CG53			
34	16168	CG53			
35	16742	CG53			
36	16123	CG53	CG 53 STATISTICS		
42	15765	CG53	AVG = 15,477		
43	14677	CG53	STD DEV = 1,177		
44	15179	CG53	MIN99 = 12,734		
45	13615	CG53			
46	12057	CG53			
47	14873	CG53			

aluminum and steel will achieve both of these objectives. This solution was employed to improve the reliability of the transition joint rings used to insert steel aircraft tiedowns into the aluminum flight decks of the Aegis class ships. The addition of titanium does not, however, eliminate the need for the 1100 aluminum interlayer. The alloying elements of 5456 aluminum are not metallurgically compatible with titanium at elevated temperatures.

In support of this need, Explosive Fabricators introduced a new transition joint product under the trade name Duratemp. The product consists of 5456 aluminum bonded to steel with both an 1100 aluminum interlayer and a titanium interlayer. Duratemp is generically referred to as trimetallic. It is manufactured in the same overall sizes and thicknesses as the bimetallic product. The term "bimetallic" actually refers to a product which has three metals (triclad); one steel and two different aluminum alloys. The term "trimetallic" similarly refers to a product which has four metals (quadclad); one steel, one titanium, and two different aluminum alloys.

The trimetallic material tests showed clearly superior properties. Tensile and shear strengths are much greater than the minimum required by MIL-J-24445, and significantly greater than the average properties of conventional bimetallic bonds. Furthermore, trimetallic remains strong to much higher temperatures 538 vs. 315°C (1000 vs. 600°F). Also, it is the only transition joint which can reliably pass the bend test of MIL-J-24445.

A decision was made to pursue implementation of trimetallic material as a long term improvement. Explosive Fabricators undertook the first article test program required by MIL-J-24445. Other manufacturers are in the process of first article testing. Appendix B lists the measured properties of first article test results for the Duratemp trimetallic product. In addition to testing of 315°C (600°F) heat treated samples as specified in MIL-J-24445, tests were also performed after a heat treatment at 538°C (1000°F) to simulate extreme conditions. Test results were so clearly superior in every respect, not only to the minimum requirements, but also to the actual bimetallic product properties, that Reference (3) approved the use of trimetallic material saying:

> "We approve this product for use on U.S. Navy ship applications which specify use of Aluminum Steel Bimetallic transition joints required in MIL-J-24445."

Independent testing at Ingalls Shipbuilding of welded trimetallic samples showed an increase in the average ultimate stress of 76%. Pertinent data from Appendix A is summarized in Table VIII. Ram tensile data taken from the first six production plates is presented in Table IX. Note that ram testing is a different method than used for our testing.

Cost Considerations

Trimetallic transition joint material costs approximately slightly more than bimetallic material. The increased material cost for the trimetallic material prompted a study of ways to reduce the cost. Four cost reduction factors are considered: strip cutting, strip width, strip thickness and welding.

Strip cutting. Both major manufacturers cut bimetallic transition joint material by sawing. They recommended against the use of the lower cost plasma cutting approach due to concerns over thermal bond degradation. This recommendation was based on tests made in the 1960°s during development of the 3.5cm (1-3/8 in.) thick bimetallic transition plates. Since that time, the material is now 45% thinner, 2cm (3/4 in.), permitting higher plasma torch travel speeds (lower heat input). In addition, the

TABLE VIII
TRIMETALLIC VERSUS
BIMETALLIC BOND STRENGTHS

UNRESTRAINED, 400°. INTERPASS

SEQ NUM	MAT'L	ULT STRESS	SEQ NUM	MAT'L	ULT STRESS
37	BI	14,773	66	TRI	22,119
39	BI	14,429	69	TRI	26,006
42	BI	15.765	71	TRI	26,544
44	BI	15;179	72	TRI	28,201
45	BI	13,615	76	TRI	25,581
48	BI	15,012	77	TRI	26,799
50	BI	13,918			
52	BI	14,676			
54	BI	14,914			

BIMETALLIC STATISTICS		TRIMETALLIC STATISTICS	
AVG =	14,698	AVG =	25,875
STD DEV=	612	STD DEV=	1,867
MIN99 =	13,271	MIN99 =	21,524

TABLE IX
EARLY TRIMETALLIC
RAM TENSILE DATA

SAMPLE NUMBER	NO HEAT TREAT	600'F TREAT	1000'F TREAT
1	24,771	24,941	18,058
2	28,046	21,339	18,764
3	27,356	**N/A**.	17,931
4	27,021	**N/A**	16,092
5	27,523	**N/A**	20,455
6	26,316	**N/A**	16,774
AVG=	26,839	23,140	18,012
STD DEV=	1,063	1,801	1,400
MIN99=	24,363	18,944	14,750

shipyards now have more powerful numerically controlled plasma cutting machines capable of sustained high travel speeds.

Because the original testing was done so long ago, some narrow test strips were plasma cut from bimetallic and trimetallic 2cm (3/4 in.) thick plates. A numerically controlled high power plasma torch cut the strips from transition plates at high speed, 89cm/min (35 IPM). The plates were *not* submerged in water (which would cool it further), but there was a normal cooling and muffling water jacket. The strips were welded to steel and aluminum bars, then pulled apart. Because of differences between the various manufacturer's bimetallic products, plasma cutting strips from one manufacturer's bimetallic plate showed 41% increase in average ultimate stress over the saw cut strips purchased from another manufacturer. Please note that all manufacturer's products exceeded the minimums required by MIL-J-24445, even after plasma cutting. Plasma cut trimetallic material was even stronger than the strongest of the bimetallic products. In fact, it was difficult during testing to disbond the trimetallic transition joint before the welds or base materials fractured. Testing showed that there was no significant difference (1%) between plasma and saw cut trimetallic strips cut from the same plate (lower part of Table X). Pertinent data from Appendix A is summarized in Table X.

Plasma cutting of the trimetallic material offers additional advantages. If bars are cut in situ, only plate need be purchased, greatly reducing current bar inventory. Plasma cutting would permit manufacture of single piece tee connections, resulting in a reduction in the number of complex butt joints.

Transition joint width. The higher strength of the trimetallic joint should permit a reduction in the strip width to aluminum plate thickness ratio. Initial calculations indicate that a ratio reduction to 3:1 may be justified.

Transition joint thickness. The improved elevated temperature performance of trimetallic transition strips should permit use of thinner transition joint components, further reducing costs (and weight).

Welding costs. An increase of permissible interpass temperatures, which should be acceptable for trimetallic bars, might result in a reduction in welding labor costs due to more productive welding processes and shorter waits for interpass cooling.

CONCLUSIONS

No single cause for the bond separations could be isolated. Several significant factors could be occurring

TABLE X
EFFECT OF CUTTING METHOD
ON BOND STRENGTH

BIMETALLIC

SEQ NUM	CUT BY	ULT STRESS	SEQ NUM	CUT BY	ULT STRESS
37	SAW	14,773	56	PLASMA	19,341
39	SAW	14,429	57	PLASMA	19,736
48	SAW	15,012	59	PLASMA	21,606
50	SAW	13,918	60	PLASMA	19,798
52	SAW	14,676	62	PLASMA	21,725
54	SAW	14,914	65	PLASMA	21,363
SUPPLIER=(DUPONT)			SUPPLIER=(EFI)		

SAW STATISTICS	PLASMA STATISTICS
AVG = 14,620	AVG =20,595
STD DEV= 364	STD DEV = 986
MIN99 = 13,771	MIN99 =18,297

TRIMETALLIC

SEQ NOM	CUT BY	ULT STRESS	SEQ NUM	CUT BY	ULT STRESS
78	SAW	20,991	66	PLASMA	22,119
79	SAW	23,400	69	PLASMA	26,006
83	SAW	26,041	70	PLASMA	21,594
84	SAW	22,744	71	PLASMA	26,544
87	SAW	29,077	72	PLASMA	28,201
			73	PLASMA	20,102
			74	PLASMA	20,192
			76	PLASMA	25,581
			77	PLASMA	26,799

SAW STATISTICS	PLASMA STATISTICS
AVG = 24,451	AVG =24,127
STD DEV= 2,826	STD DEV = 2,932
MIN99 = 17,867	MIN99 =17,296
SUPPLIER = (EFI)	SUPPLIER = (EFI)

synergistically to cause failures including:

(a) Weld heat from butt welds in strips may be weakening the bond local to the butt. The weakened area serves *as* a separation initiation point which may grow depending upon local stresses.

(b) Material properties may vary enough that some bond areas are susceptible to separation. This variation is not detectable by test methods required by MIL-J-24445.

(c) Strip widths may not be sufficient to meet manufacturers recommendations and to compensate for (a) and (b) above.

(d) Welding methodology, such as using shielded arc welding, could cause overheating and bond weakening.

The study showed that trimetallic transition joints greatly improve the reliability while offering potentially lower overall costs.

RECOMMENDATIONS

MIL-J-24445

The government may want to revise this document to reflect current technology (trimetallic), sampling from the weak areas of the plate (the initiation point) and incorporating statistical requirements for properties (MIN99).

DESIGN

Statistical knowledge of actual strengths of welded transition joint and structural plating should be considered. in establishing design guidelines. If a 1% disbond rate is considered acceptable, the recommendation based on data reported in this paper would be to provide bimetallic strip widths of 4.24 times the thickness of the aluminum plating. Minimum widths of the trimetallic material would be on the order of 3 to 1. These recommendations may be modified to take into account the width of weld fillets and needed reliability at strip butts.

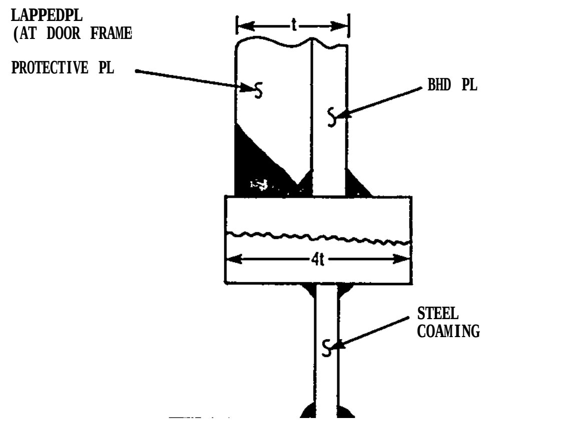
The designer should always specify a partial penetration butt design (as shown in Figure 4) and should give preference to designs which minimize butt welds. See Figure 7 for some ideas.

PRODUCTION

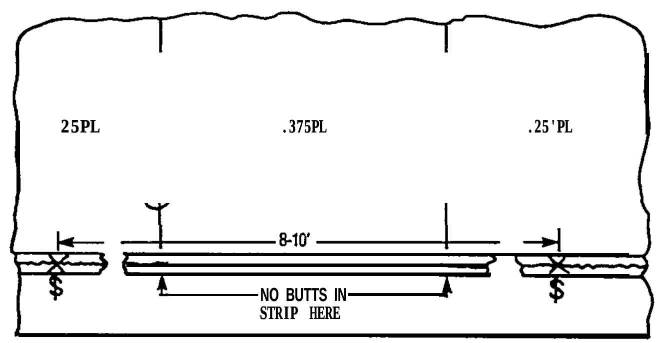
The peak bond joint temperature of bimetallic transition joints should be limited to 31 C (600°F) This *can* be done in production by prohibiting SMAW welding (tacking is OK) and limiting the interpass temperature tO a maximum of 204°C (400°F). Colder weld processes (short arc & pulsed arc) are slightly preferred over the more normal (spray arc) GMAW and FCAW processes, but all are acceptable. Care should be exercised to ensure full penetration butt joint designs are not substituted for partial penetration butt designs in the strips. The number and proximity of butt welds should be minimized. When plasma cutting, the highest feasible travel speed should be used. Submerging the transition joint plate in a water table may be beneficial to bond strength and to minimize thermal distortion of the strips. Periodic tensile and bend testing of plasma cut strips would be a wise precaution. Samples should be cut near the initiation point, if that is known.

SUMMARY

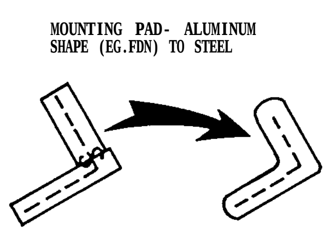
Some aluminum to steel bimetallic transition joints were disbonding in ships under construction and, to a lesser extent, in the fleet. This was unusual because the strips were designed to be stronger that the aluminum plating



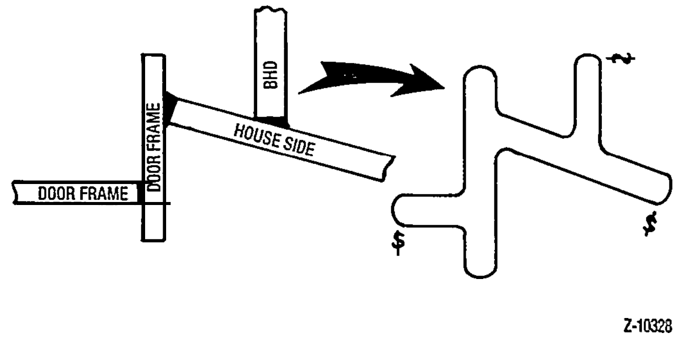
1. WHERE PLATE HAS LAPPED PLATE DESIGN STRIP WIDTH SHOULD CONSIDER TOTAL THICKNESS



2 MINIMIZE BUTTS USE FULL LENGTH STRIPS (8-10 TYP) WITH WIDTHS SIZED TO THICKER PLATING



3 SIMPLIFY BY PLASMA CUTTING PADS FROM PLATE



4. SIMPLIFY BY CUTTING COMPLEX INTERSECTION WITH PLASMA

Figure 7. Some designer recommendations

attached. A study was undertaken to determine the causes and recommend corrective measures. Several possible causes were found, some eliminated, and preventative measures instituted. The most significant improvements were in design and materials. During the course of the study, a new trimetallic aluminum to steel transition joint was introduced and certified. The trimetallic design provides higher strength and higher resistance to degradation during installation while offering potentially lower overall costs.

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3. "Approval of Duratemp First Article Testing to MIL-J- 24445", memo from NAVSEA 05M2/151, June 7, 1989.

4. "Bonding of Metals with Explosives" by A. H. Holtzman and G. R. Cowan, Welding Research Council Bulletin #104, April 1965.

APPENDIX A
ISD TENSILE TESTS

SEQ NUM	REST-RAINT	WIDTH	LENGTH	ULT LOAD	ULT STRESS	INTERPASS MAX TEMP	HULL MATL	FAIL AT
5	R	1.255	3.333	34300	8200	150`F	CG53	AL-STL+WELD
10	R	1.003	3.180	41000	12855	150`F	CG53	AL-STL
13	U	0.991	3.297	50900	15578	150`F	CG53	AL-STL
14	R	0.990	3.367	45300	13590	150`F	CG53	AL-STL
15	U	1.001	3.330	56200	16860	150`F	CG53	AL-STL
16	R	0.989	3.280	45300	13965	150`F	CG53	AL-STL
17	U	1.001	3.331	52600	15775	150`F	CG53	AL-STL
18	R	0.989	3.313	52700	16084	150`F	CG53	AL-STL+WELD
19	R	0.987	3.356	52600	15880	150`F	CG53	AL-STL
20	U	1.009	3.372	56200	16518	150`F	CG53	AL-STL
21	R	0.989	3.279	52700	16251	150`F	CG53	AL-STL
22	U	0.991	3.256	51400	15930	150`F	CG53	AL-STL
23	R	1.003	3.361	54900	16286	150`F	CG53	AL-STL
24	U	0.987	3.304	52000	15946	150`F	CG53	AL-STL
25	U	0.990	3.380	54500	16287	150`F	CG53	AL-STL
26	RW	0.990	3.350	52800	15920	150`F	CG53	AL-STL
27	uw	0.994	3.276	49700	15263	150`F	CG53	AL-STL
28	R	0.988	3.214	51200	16124	150`F	CG53	AL-STL
29	uw	0.978	3.270	45400	14196	150`F	CG53	AL-STL
30	RW	0.984	3.336	54100	16481	150`F	CG53	AL-STL
31	U	0.989	3.267	51500	15939	150`F	CG53	AL-STL
32	R	0.995	3.325	55100	16655	150`F	CG53	AL-STL
33	uw	0.987	3.240	51800	16198	150`F	CG53	AL-STL
34	RW	0.984	3.256	51800	16168	150`F	CG53	AL-STL
35	uw	0.987	3.268	54000	16742	150`F	CG53	AL-STL
36	RW	0.998	3.300	53100	16123	150`F	CG53	AL-STL
37	U	0.997	3.191	47000	14773	400`F	CG65	AL-STL
38	R	0.992	3.231	46500	14508	400`F	CG65	AL-STL
39	U	0.994	3.291	47200	14429	400`F	CG65	AL-STL
40	R	0.993	3.275	46400	14268	400`F	CG65	AL-STL
41	R	0.989	3.284	45400	13978	400`F	CG65	AL-STL
42	U	0.976	3.334	51300	15765	400`F	CG53	AL-STL
43	R	0.990	3.207	46600	14677	400`F	CG53	AL-STL
44	U	0.976	3.267	48400	15179	400`F	CG53	AL-STL
45	U	0.982	3.291	44000	13615	400`F	CG53	AL-STL
46	R	0.991	3.289	39300	12057	400`F	CG53	AL-STL
47	R	0.988	3.287	48300	14873	400`F	CG53	AL-STL
48	U	1.002	3.304	49700	15012	400`F	CG65	AL-STL
49	R	0.987	3.244	45900	14336	400`F	CG65	AL-STL
50	U	0.993	3.285	45400	13918	400`F	CG65	AL-STL
51	R	0.981	3.350	46500	14149	400`F	CG65	AL-STL
52	U	0.994	3.311	48300	14676	400`F	CG65	AL-STL
53	R	0.993	3.241	46000	14293	400`F	CG65	AL-STL
54	U	0.998	3.225	48000	14914	400`F	CG65	AL-STL
56	U	1.179	3.346	76300	19341	PLASMA CUT	TEST	AL-STL
57	U	1.177	3.289	76400	19736	PLASMA CUT	TEST	AL-STL
59	U	1.172	3.333	84400	21606	PLASMA CUT	TEST	AL-STL
60	U	1.175	3.267	76000	19798	PLASMA CUT	TEST	AL-STL
62	U	1.170	3.344	85000	21725	PLASMA CUT	TEST	AL-STL
65	U	1.171	3.118	78000	21363	PLASMA CUT	TEST	AL-STL
66	U	1.118	3.417	84500	22119	TRI/PLASMA	TEST	TI-ST
69	U	1.150	3.210	96000	26006	TRI/PLASMA	TEST	ALL AL
70	UN	1.250	2.145	57900	21594	TRI/PLASMA	TEST	TI-ST
71	U	1.152	3.231	98800	26544	TRI/PLASMA	TEST	1100 AL
72	U	1.150	3.438	111500	28201	TRI/PLASMA	TEST	1100 AL
73	UN	1.134	2.812	64100	20102	TRI/PLASMA	TEST	1100 AL
74	UN	1.290	2.361	61500	20192	TRI/PLASMA	TEST	1100 AL
76	U	1.160	3.370	100000	25581	TRI/PLASMA	TEST	TI-ST
77	U	1.120	3.265	98000	26799	TRI/PLASMA	TEST	1100-5456
78	UN	1.190	2.450	61200	20991	TRI/SAW	TEST	1100-5456
79	UN	1.175	2.375	65300	23400	TRI/SAW	TEST	1100-5456
83	UN	1.255	1.995	65200	26041	TRI/SAW	CG72	ALL AL
84	UN	1.248	2.142	60800	22744	TRI/SAW	CG72	1100 AL
87	UN	1.257	1.855	67800	29077	TRI/SAW	CG72	1100 AL

APPENDIX B
EFI TRIMETALLIC FIRST ARTICLE TESTING

TEST TYPE	PRE-HEAT PEAK/HOLD	BOND ZONE TESTED	SAMPLE FROM *	REQUIRED RESULT	ACTUAL RESULT	REQD. BY MIL SPEC,
TENSILE	NONE	ALL	IE	11,000	26,847	Y
"	NONE	ALL	TE	11,000	31,685	Y
"	600 DEG	ALL	IE	11,000	24,941	Y
"	600 DEG	ALL	TE	11,000	21,339	Y
"	1000 DEG	ALL	IE	11,000	20,660	N
"	1000 DEG	ALL	TE	11,000	21,371	N
SHEAR	NONE	AL-TI	IE	8,000	14,687	Y
"	NONE	AL-TI	IE	8,000	15,448	Y
"	NONE	AL-TI	IE	8,000	15,939	Y
"	NONE	AL-TI	TE	8,000	16,681	Y
"	NONE	AL-TI	TE	8,000	16,292	Y
"	NONE	AL-TI	TE	8,000	16,411	Y
"	NONE	TI-ST	IE	8,000	40,523	
"	NONE	TI-ST	IE	8,000	45,737	f
"	NONE	TI-ST	IE	8,000	43,001	Y
"	NONE	TI-ST	TE	8,000	42,870	Y
"	NONE	TI-ST	TE	8,000	47,350	Y
"	NONE	TI-ST	TE	8,000	46,079	Y
"	600 DEG	AL-TI	IE	8,000	16,630	Y
"	600 DEG	AL-TI	IE	8,000	14,292	Y
"	600 DEG	AL-TI	IE	8,000	14,650	Y
"	600 DEG	AL-TI	TE	8,000	15,104	Y
"	600 DEG	AL-TI	TE	8,000	14,552	Y
"	600 DEG	AL-T-I	TE	8,000	14,292	Y
"	600 DEG	TI-ST	IE	8,000	42,361	Y
"	600 DEG	TI-ST	IE	8,000	37,786	Y
"	600 DEG	TI-ST	IE	8,000	43,324	Y
"	600 DEG	TI-ST	TE	8,000	47,317	Y
"	600 DEG	TI-ST	TE	8,000	51,874	Y
"	600 DEG	TI-ST	TE	8,000	50,391	Y
".	1000 DEG	AL-TI	IE	8,000	16,505	N
"	1000 DEG	AL-TI	IE	8,000	14,732	N
"	1000 DEG	AL-TI	IE	8,000	15,618	N
"	1000 DEG	AL-TI	TE	8,000	18,062	N
"	1000 DEG	AL-TI	TE	8,000	19,318	N
"	1000 DEG	AL-TI	TE	8,000	17,726	N
"	1000 DEG	TI-ST	IE	8,000	39,144	N
"	1000 DEG	TI-ST	IE	8,000	39,359	N
"	1000 DEG	TI-ST	IE	8,000	40,829	N
II	1000 DEG	TI-ST	TE	8,000	39,144	N
II	1000 DEG	TI-ST	TE	8,000	39,359	N
II	1000 DEG	TI-ST	TE	8,000	40,829	N
WELDED TENS	NONE	ALL	MIDDLE	23,000	26,100	Y
II	NONE	ALL	MIDDLE	23,000	25,000	Y
FATIGUE			MIDDLE			
-15/+5,150KC	NONE	ALL	MIDDLE	PASS	PASS	Y
-15/+1,650KC	NONE	ALL	MIDDLE	PASS	PASS	Y
-10/+3,1MC	NONE	ALL	MIDDLE	PASS	PASS	Y
SIDE BEND	NON-E	ALL	IE	PASS	PASS	Y
II	NONE	ALL	TE	PASS	PASS	Y
II	600 DEG	ALL	IE	PASS	PASS	N
11	600 DEG	ALL	TE	PASS	PASS	N
M	1000 DEG	ALL	IE	PASS	PASS	N
II	1000 DEG	ALL	TE	PASS	PASS	N
CHISEL	NONE	ALL	IE	PASS	PASS	Y
II	NONE	ALL	TE	PASS	PASS	Y
1,	600 DEG	ALL	IE	PASS	PASS	N
II	600 DEG	ALL	TE	PASS	PASS	N
11	1000 DEG	ALL	IE	PASS	PASS	N
1,	1000 DEG	ALL	TE	PASS	PASS	N

* IE IS INITIATION POINT END, TE IS OPPOSITE (TRAILING) END

Ivo Fioriti, PE, Retired from NAVSEA

I have read your paper with a degree of sadness because you discuss problems which should not have been and did not occur while I was in charge of the development throughout the late 60's and all of the 70's. The people involved with the development then are long gone. If there are problems, the new breed of engineers that replaced them may not have maintained the same high quality levels necessary in manufacture and fabrication to avoid bond separations. I remain confident that the problems can be solved once the underlying causes of bond separations become known.

Selling a new, radical concept for shipbuilding, particularly Navy, is a very difficult task indeed. So it was with the transition joint material. Therefore, the participants were very careful in their role during the development. The Navy subjected the transition joint material to severe testing (beyond service performance needs) like explosion bulge testing, structural beam fatigue to very high stresses, thermal fatigue, corrosion and the many small scale mechanical tests. At no time did bond separation become a problem. This work was done on the Dupont detaclad joint and the Revere Copper & Brass roll bonded joint. For all follow on producers of the transition joint material, the qualification tests were reduced to the small scale mechanical tests. Northwest Technical qualified later on the basis of small scale mechanical tests a short time before I retired from Navy. Explosive Fabricators {qualified after I left}. The secrets of the successful development, in short, were three fold: (1) The Navy's tortuous qualification testing of the transition joint. (2) The manufacturer's production knowledge of what was well bonded material and what was not. Through an in-house NDT (which was correlated with mechanical bond strength tests), the manufacturer knew what was good and bad, and only sold good transition joint material to Navy and shipbuilders. (3) The shipbuilders were well aware of the effects of welding heat degradation of the joint material and instituted well supervised safeguards to avoid surpassing the 600°F limit.

The MIL-J-24445 specification is not a sacred cow! After my retirement from NAVSEA, there were people at NAVSEA revising the specification who had no experience with the transition joint material. Also, because the specification covers explosive bonded, roll bonded and any other new procedure than can qualify, it cannot institute a NDT bond procedure across the board that applies equally well to all the joint materials. Therefore, the in-house NDT technique that Dupont or Revere Copper & Brass used to furnish well bonded material never got into the specification as a detailed requirement. However, the bonding and quality control procedures, and materials used by the manufacturer in obtaining qualification became a requirement of the Navy approval letter. The letter states that the manufacturer shall use the same procedures/materials that were approved for the production of material to be offered under the Mil. Spec.. Any changes to those procedures are subject to Navy approval and may be the subject of re-qualification. Therefore, it is not correct to say the transition strip must only meet the Mil. Spec.. At this late date, it would be interesting to compare a manufacturer's present procedures/materials with those that were used way back then to obtain qualification approval. If poorly bonded material is being received in the shipyard, the first thing would be to review the responsible manufacturer's production procedures/materials, and second review the Mil. Spec. for weaknesses and improvements to alleviate the problem. As part of this same study, all of the Mil. Spec. revisions issued from the first to the present should be reviewed to document technical requirements and changes to determine if the specification was strengthened or weakened over the years. Your paper does not address these items and they are at the heart of any bond separations.

What you have found out about UT inspection was well known 20 years ago at the start of the program. UT inspection in the specification can only provide protection against poorly bonded material that is on the verge of forming a lamination. That is why the Dupont in-house NDT quality control procedure was not based on UT.

Documentation of all bond failures in detail is essential by the shipbuilders and Navy so that the cause(s) can be determined and rectified. If the bond failures can not be solved, then there is a problem. However, most likely, the cause becomes known and there is no good reason to fault the transition strip material. Your paper does not go into this and you are trying to find a cause for the bond separations.

All of the bonded material you welded and tested should have received a valid NDT quality control procedure (like DuPont's in-house control) before hand. In this manner you may have been able to explain some of your results, especially as to variations in terms of bond quality.

In reviewing all of your tensile test

data in the tables, I haven't found any values that do not meet the Mil. Spec. minimum tensile strength requirement of 11,000 PSI. Therefore, the tensile data do not prove you have a bond strength problem. In fact, in Table IV with a maximum interpass temperature of 400°F, the data still meets the requirement. Each table should show the Mil Spec. requirement. It is very important to include the above tensile test data findings as a conclusion in your paper.

On strip widths, your analysis is academic using the wrong numbers. The 4 to 1 design rule is more than adequate and conservative. Also, you need a proper landing area for the aluminum fillet welds and a little lee way on fit-up where the deckhouse plating does not land in the exact center of the transition strip. This area need is also required for the trimetallic strip.

On restraint, you do not show the restraining fixture and you have not measured residual stresses. Nor do you know what the variation in bond quality (meaningful NDT method) is. The two groups of data look very good! Normally, in welding configurations of this type, you would not expect restraint to be a factor because of the thin plating thicknesses. small fillet welds and the low yield strength of the 1100 aluminum alloy. Note, I said "yield strength" and you said "tensile strength".

There is not enough time and ships represented to make much of the data in Table VIII. Studies of this type should continue into the future as more ships are built. All the numbers look good, well within Mil. Spec. and probably are comparable to the old material of the 70's.

The trimetallic development is significant where improved bond strength is needed. But your paper does not prove that greater bond strength is needed. So why do you want to go to a more expensive material? Note the high speed plasma cutting (which can save money) in Table X can also be made to apply to the bimetallic and results in a substantial improvement in bond strength. The improved elevated temperature performance of the trimetal does not necessarily translate into thinner transition strips. In going to thinner steel and aluminum, other problems begin to arise like distortion/flatness tolerances of the material prior to bonding and distortion of the thinner strips during fabrication in the shipyard (higher temperatures + thin material = distortion). Also, the 20% price increase for the trimetal, most likely, is a foot in the door price. It would not surprise me if the actual increase becomes 30 to 40%. Finally, I would expect the Navy to permit use of both the bimetal and trimetal strips on an equal basis in the Ship Design Specifications. In the end, it will be the shipbuilder who decides what to use and cost will control.

On the trimetal qualification for ship service I would say that MIL-J-24445 is not entirely adequate. The addition of titanium in the sandwich introduces a material that is more noble in the electro-motive series than steel and aluminum. Therefore, the concern for corrosion becomes greater. MIL-J-24445 was intended only for steel/aluminum joint strips, which were proven satisfactory in corrosion tests during development. The corrosion tests were not considered necessary in the MIL-SPEC for qualification, as long as the materials were steel and aluminum. Recommend corrosion tests be performed on bare surface and painted surface specimens: (1)salt air, (2) salt air fog, (3) salt water spray and (4) intermittent salt water immersion (heavy seas/main deck awash).

Your designer recommendations in figure 7 were recommended in papers/literature prior to first use of detaclad. If a shipbuilder has not been following your recommendations from day one, its his own fault.

In Appendix A, the failure location is not clear. Suggest you use a cross section of the complete joint showing the various fracture paths. If you got bond failures, this is something different from the early work.

For your information, I would like to mention the good points of the Revere roll bonded bimetal. The Revere bimetal exhibited (over detaclad {bimetallic)) slightly higher fatigue strengths in large scale structural beam tests, showed a greater tolerance for the heat of welding and was responsible for the reduction in thickness of the Dupont bimetal from 1 3/8" to 3/4", has the potential of producing longer strips to reduce the number of butt joints, the potential of being made in thinner thicknesses, and the potential for a more uniform quality level by closer control of manufacturing variables. After development, the shipbuilders were at fault for not accepting the roll bonded bimetal as an equal to the detaclad. Detaclad was specified on the drawings and ship specifications and it would have cost money to change them. They had worked out fabrication procedures for detaclad and would have to do the same for roll bonded. Running scared on such an important joint, many simply did not trust the roll bonded material. So Revere never got enough business to get out of the pilot plant stage. The shipbuilders and the Navy in the long run-were the real losers: The competition would have been good for improve-

ments, quality and cost savings.
If you have any questions on what I have written, I suggest you get in touch with Chuck McKenny (who is now a consultant), Allen Manuel NAVSEA55Y3 for the old files if he still has them, and Revere Copper & Brass- Rome N.Y. office.
Lots of luck to you!

Sincerely,
Ivo Fioriti

Attachment 2

Discussion by Ivo Fioriti, PE, 2932 Fairhill Rd., Fairfax, VA 22031 (703) 560-2357 dated 30 April, 1990

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